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PROGRESS IN THE INTRODUCTION OF SHELL MOLDING AT A SPECIALTY STEEL FOUNDRY

By R. J. Cowles and D. C. Ekey***

Introduction

Shell molding has matured beyond the initial controversies of acceptance with which any new process is plagued. The applications which require the least technological developments have, of course, progressed the most rapidly. In addition, shell molding progress has been most successful in the larger foundries which can support the development costs inherent in the conversion of the process to competitive production.

The basic principles of the shell process appear so simple and the possibilities for molding improvements are so apparent that the first reaction of most foundries is to immediately accept the process for a wide range of castings. The technology, however, is more involved and in many cases a period of development and research is necessary to establish successful acceptance. There is often, then, a reaction of opposition when shell molding is installed and given only a cursory trial study. This is particularly true in the case of a specialty jobbing steel foundry such as Lebanon Steel Foundry where customer orders are predominately short run. Long run production items can better support the initial pattern costs and pattern changes necessary to produce shell molded castings successfully.

A further limitation of the shell process for steel foundries, and one very vital to the acceptance of continued growth of the shell process, is the problem of gas "pocks" formed on the surface of low carbon and low alloy steel castings poured in shell molds. Since exceptionally smooth, excellent quality castings can be made in shell molds from high alloy and high carbon steels, as well as grey iron and nonferrous metals, it would seem that this difficulty could be obviated without too much effort. Thus far, however, a general or universal solution of this problem has not been made known. In fact, the knowledge thus far made available to the steel foundries presents a profusion of conflicting results and indicates that the interrelation of many variables are difficult to control. Experimental results to date, therefore, have been confusing and not easily interpreted.

The "Pocking" Problem

The apparently slow progress of any one group in determining a satisfactory answer to the problem, here designated as "pocking", does not indicate an inept or low quality research effort. Many laboratories of both the resin producing companies and the steel foundries have considered this problem and have found the answers both elusive and inadequate. Lebanon Steel Foundry is no exception. A great number of experimental test series have been carried out with the aid of several consultants. The results, as well as those presented in Research Report No. 34 of the Steel Founders Society, however, show enough progress to encourage the continuation of the research effort to produce high quality low carbon and low alloy steel castings in shell molds. At present these "pocking" metals constitute a major percentage of the casting weight sold by the authors' company. Many of the high alloy castings are too large to consider producing in shell molds. Production is now limited to shell molding in high alloy castings weighing less than 70 pounds and ordered in quantities of approximately 2000 or more. The exceptions are those which are competitive with precision molding methods and can demand prices high enough to justify the higher patttern costs when ordered in comparatively small quantities.

The deterrent to the objective of the research efforts to produce high quality low carbon and low alloy steel castings in shell molds is the rough "pocked" casting surface resulting from the physical-chemical interactions between the pyrolytic mold products and the molten metal during surface solidification. The results of the research effort discussed herein are:

1. The variables encountered.
2. The many hypothesis which have been suggested in an effort to clarify the "pocking" phenomenon.
3. The research approaches designed to extend our knowledge and possibly determine the problem solution.
4. A correlation of results and their value to date.

It is continually necessary to carefully specify the limits of the variables which led to the research conclusions. For instance, one set of conditions will establish a relationship, (i.e.—the greater the mold

* Formerly Research Engineer, Lebanon Steel Foundry, now Research Engineer, Walworth Company.
** Technical Director, Lebanon Steel Foundry.

permeability, the less "pocking")—and then in another group of experiments no such relationship exists. There are, of course, different interrelations of variables existing to cause such confusing results.

Figure 1 shows the appearance of the undesirable casting surface designated as "pocking" and Figure 2 shows the same casting poured with metal from the same heat into a similar mold but with one change — that of metal pouring temperature. It

Figure 1—The undesirable casting surface condition designated here as "pocking." Metal pouring temperature of 3030 degrees F to 3080 degrees F.

has been established that pouring temperature is an important variable and the indications shown by this study are that a low pouring temperature, such as 2800 degrees F, is the best method for preventing "pocking" in low carbon steel. This may not always prove true, however, since the casting size, method of gating (including runners) and risering are important variables that must be considered. It is noted on page 37 of S.F.S.A. Research Report No. 34, "The Shell Molding Process for Plain Carbon and Low-Alloy Steel Castings," that "High pouring temperatures, in the range of 3000 degrees F, are also advantageous in improving surface appearance and sharpness of detail". This conclusion applies to the conditions used in that investigation, and apparently it is in direct opposition to several test series recently conducted. The results of another series of tests, made approximately eighteen months ago, agreed with those of Report No. 34 in regard to the advantage of employing high pouring temperatures. It becomes dangerous, then, to attempt to correlate the apparent conclusions of one experiment with those of another, unless all variables are held constant.

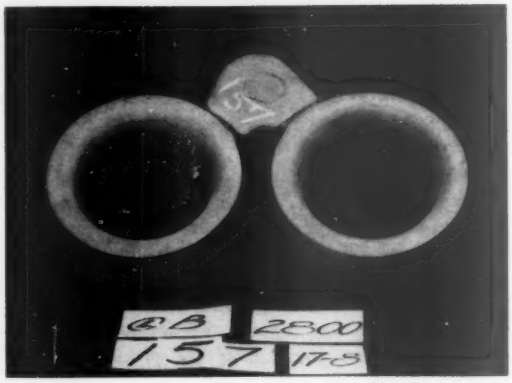


Figure 2—The improved casting surface over Figure 1, due only to reduction in metal pouring temperature (2800 degrees F to 2830 degrees F).

More than 3000 experimental castings in shell molds were poured under many controlled conditions and with numerous variations in mold coatings, mold additives and mold materials such as sand and binder, as well as practices of metal preparation and pouring. The approach has not been classical but rather empirical with a trial and error guidance from one set of conditions to the next. Although this is time consuming, the results have been favorable enough to eliminate "pocking" on a number of low carbon and low alloy items now produced for sale. Figure 3 illustrates the appearance of several "pock" free castings made by the shell mold process.

Figure 3—Low carbon and low alloy castings which have been cast in shell molds with a minimum of undesirable "pocking".

Hypothetical Analysis of The Pocking Phenomenon

The favorable results of this research together with the knowledge gained by the negative results have established a basis for certain hypothetical explanations for the "pocking" occurrence. Excess gases are present at the mold surfaces when the hot flowing metal comes in contact with the resin-sand surface. The cooling outside film of the metal solidifies simultaneously with this gas formation, a condition which can cause "pocking" impressions in the casting surface. Fig re 4 shows a metal skeleton surface of low carbon steel composition produced by causing a "run out" just as the surface metal was solidifying. There are apparent areas of "pocking" or holes similar to the "pocking" distribution and size which indicate that this condition can be produced at an early stage in forming the casting.

Figure 4—A metal skeleton surface of low carbon steel composition produced by causing a "run-out".

The studies of this report included the observance of a number of varying parameters and the determination of their interrelationships. The inability to visualize the activity at the metal-mold interface impels us to imagine what might logically be occurring, and then by this hypothetical analysis we can control the suspected significant variables in order to minimize or prevent the "pocking."

The variables which appear to be controlling factors of the "pocking" phenomenon are discussed below with an atttempt to point out which of these seem most important according to the experimental results.

Metal Pouring Temperature

The temperature of the metal as it is poured into the mold (and more important perhaps is the temperature increment between this pouring temperature and the liquidus temperature of the metal) contributes to the relationship between the metal solidification and the mold pyrolysis. This determines not only the length of time that the metal remains molten or mobile at any point on the mold surface but also the quantities of gas evolved simultaneously.

Properties of the Metal

Closely related to this timing relationship are such properties of the metal as: degree of surface oxidation; ability to retain fluidity or flowability previous to solidification; pressure of the metal against any mold surface at the time of solidification and the surface tension of the metal at the mold surface. These properties will vary, of course, with metal composition, melt practices, the extent of deoxidation, and the gas content.

Mold Variables

The quantities and composition of the resin binder can establish the rate, the amount, and kind of gases evolved at the metal-mold interface. There is the possibility that the gases are released and expanded by the heat so rapidly that there is in effect an explosive pressure against the molten metal. The gases may react with the surface film of the molten metal, and in the case of low carbon and low alloy steels this reaction causes an accelerated solidification resulting in the surface "pocks."

The knowledge that "pocking" occurs only in certain areas of the casting indicates that it is possible for the gas to be pressed away from some areas before solidification, thus allowing the metal to come back to the smooth, refractory carbonaceous residue formed by the pyrolysis of the mold in a reducing atmosphere. In these areas the casting is smooth with no "pocking." A series of tests showed the composition of the gases released by thermal decomposition of shell mold material at 2500 degrees F to be of the composition shown in Table I.

The large quantities of hydrogen assure a reducing atmosphere; however, it is also possible that the hydrogen present may be reactive with the metal surface.

If the gases are pressed back as proffered above, they probably move either along the mold surface or pass through the permeable mold itself. The use of fine vent releases for these gases is often beneficial for the reduction of "pocking." Internal pressure by excess gas in the metal has provided smooth outside surfaces on castings which ordinarily "pock". Failure to add the deoxidizer, aluminum, resulted in a smooth casting filled with internal gas voids.

TABLE I

Element	Percentage
Hydrogen	41 to 56
Carbon Monoxide	19 to 25
Carbon Dioxide	1 to 7
Methane	0 to 7
Oxygen	0.3 to 7
Nitrogen	Remainder

Other properties of the mold to consider are: density, thickness, degree of cure, permeability, thermal conductivity and rate of resin decomposition. The size and kind of sand as well as its distribution may be important. More important, however, is the advantage of a strongly bonded mold surface which can best be attained by using adequate resin and a sand grain distribution which affords optimim packing. Since zircon sand is different from silica sand in both thermal conductivity and density, its use as a shell molding material should be studied in the light of these parameters.

The pH of the mold surface is another variable to consider and since sprays, washes, dip coatings and additives affect the surface pH it is possible that the variation of the surface pH is important.

Particles of impurities such as slag or "washed" resin sand grains which accumulate on the metal surface next to the mold appear to aggravate the "pocking." The gases may be drawn to these areas and it is possible that "hot spots" are created which may cause the metal surface to solidify too late to prevent gas penetration into the surface.

Mold additives, mold washes, and mold solutions which are sprayed over the mold surface are continually being studied in relation to "pocking." These materials may cause such effects as:

1. Actually acting as a protecting barrier between the molten metal and the gases formed in the mold.
2. A rapid cooling of the molten metal surface by energy absorption due to changes in state such as chemical changes, heat of fusion or heat of decomposition.
3. Changing the thermal conductivity.
4. Accelerating or retarding the gas evolution during mold decomposition.

Under the experimental conditions it has been found that when low carbon steel is poured into a cooled shell mold there is less apt to be "pocking" than if the mold is heated and the assembly insulated. This seems logical since the objective is to solidify the molten metal surface before sufficient quantities of gases are accumulated to develop pressures capable of moving the metal away from the mold as the metal surface solidifies.

In the case of high alloy steels the objective is just the reverse, that is, to pour the metal as high above the liquidus temperature as possible so that the gases enter the metal volume at the surface or are pressed by the metal back through the mold wall, vents, or sprue openings before the metal starts to solidify.

Figure 5 illustrates the smooth casting surfaces obtained with high alloy steel poured in shell molds.

Figure 5—The smooth casting surface obtainable with high alloy steel poured in shell molds.

Pattern Planning

The studies concerned with the mold parameters, must take a secondary role when we consider the

configuration of the castings and their relation to methods of contact gating, runner, sprue and riser design. It has been found that pattern planning is most important in the casting of metals subject to "pocking" in shell molds.

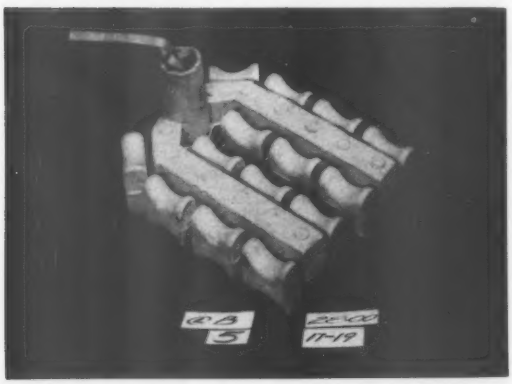
Castings containing adjacent heavy and light sections, or rectangular, chunky configurations present the most difficult problems in the elimination of "pocking" by pattern planning. Even in these cases the two variables which are considered first in alleviating "pocking" are metal temperature at pour and design of pattern assembly.

The above hypothetical explanations for the "pocking" occurrence have contributed in establishing the following thoughts:

1. The configuration of the casting is related to the reduction of "pocking" according to its ability to provide for unbroken directional solidification. The open impeller casting shown in Figure 3 is an example of a design that requires very little care to produce a good quality casting in metals that ordinarily "pock."

2. The castings illustrated in Figure 6 show that heavy sections which remain hot at the mold surface over a longer period are more apt to "pock." These volumes should be filled quickly and then kept free of turbulence especially at the mold-metal interface. This is sometimes accomplished by supplying heavy feed sections next to the casting but with a "necked-down" contact gate. The "pocking" then seems to concentrate outside the casting in the heavy runner and gating sections, especially on the bottom of the sprue mold which is subject to turbulence and continued exposure to the hottest metal. Figures 6 and 7 compared with Figure 8 present the development changes and design changes which allow the continual production of comparatively "pock" free castings of a chunky dog bone design. The fluted neck-down contact gating, adequate runners and a sprue feed large enough to fill the runners quickly all contributed to the final success. Fourteen castings in one shell mold provide an economic production.

3. One of the disadvantages of horizontal pouring, rather than vertical pouring of "pocking" metals, is the possibility of the upper, exposed portion of the mold starting to break down due to the heat forming gases before the metal ever reaches the cope mold surface. Figure 9 shows this condition quite clearly. The final satisfactory casting system, see Figure 8, alleviated most of this condition. Accelerated filling of the runners is helpful as long as turbulence in the casting is minimized. Turbulence of the molten metal at the mold surface prevents surface skin solidification, allowing more gases to form and penetrate the movable metal resulting in "pocking." The advantages of vertical pouring are shown in Figures 10 and 11. The disadvantage of vertical pouring is the increased need for "backing" the shells.



Figures 6 and 7—The initial attempts in pattern planning to produce castings of a chunky dog bone design in low carbon steel, poured in shell molds.

Figure 8—The later design used to continually produce comparatively "pock" free castings, 14 in each mold.

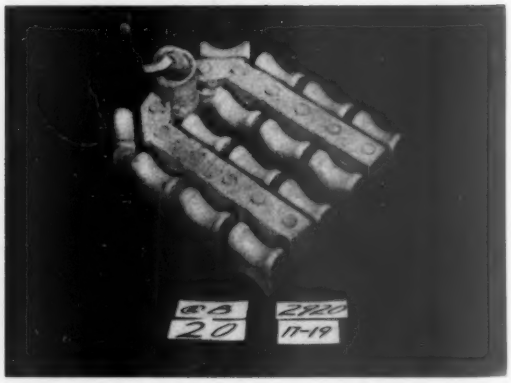


Figure 9—The result of cope surface deterioration from hot gases before molten metal actually reaches this surface.

Figure 10—The unsuccessful attempt to produce "pock" free low carbon castings by horizontally pouring.

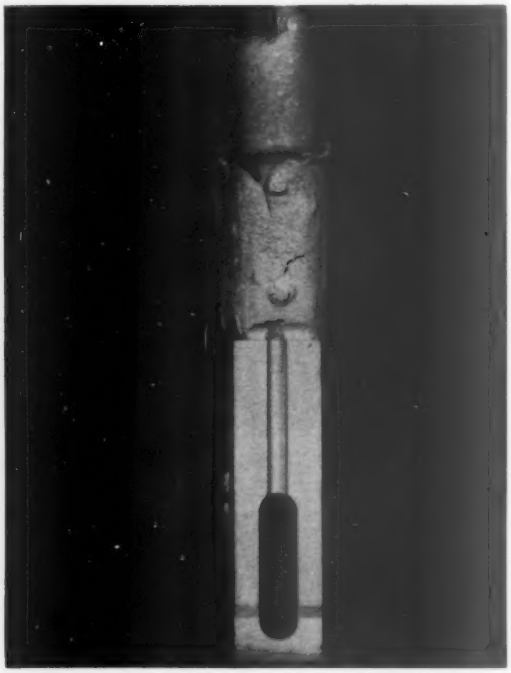


Figure 11—The advantages of vertically pouring certain low carbon castings to prevent "pocking" in shell molds.

RESEARCH PROGRAM
PROGRESS REPORT ON ACTIVE PROJECTS

Research Project No. 41

The Influence of Design On Hot Tearing of Steel Castings

The Technical Research Committee was cognizant that research on hot tearing in steel castings had been mainly devoted to attempts at explaining the mechanism of failure. Progress had been made in this respect but little information of direct practical value had been obtained. The Committee felt that present knowledge of steel casting design in relation to the prevention of hot tearing is largely empirical and experience plays a great part.

The Technical Research Committee felt there was a need for some practical information on the effect of design on the incident of hot tearing. Therefore, a two year program of research was submitted to Massachusetts Institute of Technology. This program was divided into two parts; Phase I dealt with section changes involving cylindrical shapes and section changes involving rectangular bars; Phase II dealt with T sections.

The work on Phase I has been completed and the progress report herein relates only to this phase.

The test casting employed in this research had a 17-inch bar of 2-inch diameter. The bar terminated in a flange and was run from one end through a blind riser feeding a 2½-inch diameter boss; the latter adjoining the 2 inch bar. The flanges were incorporated in the boss and the whole assembly could be constrained at any interval of time after pouring by varying the dimensions of a gap between strain bars and flanges. The basic design is illustrated in Figure 1.

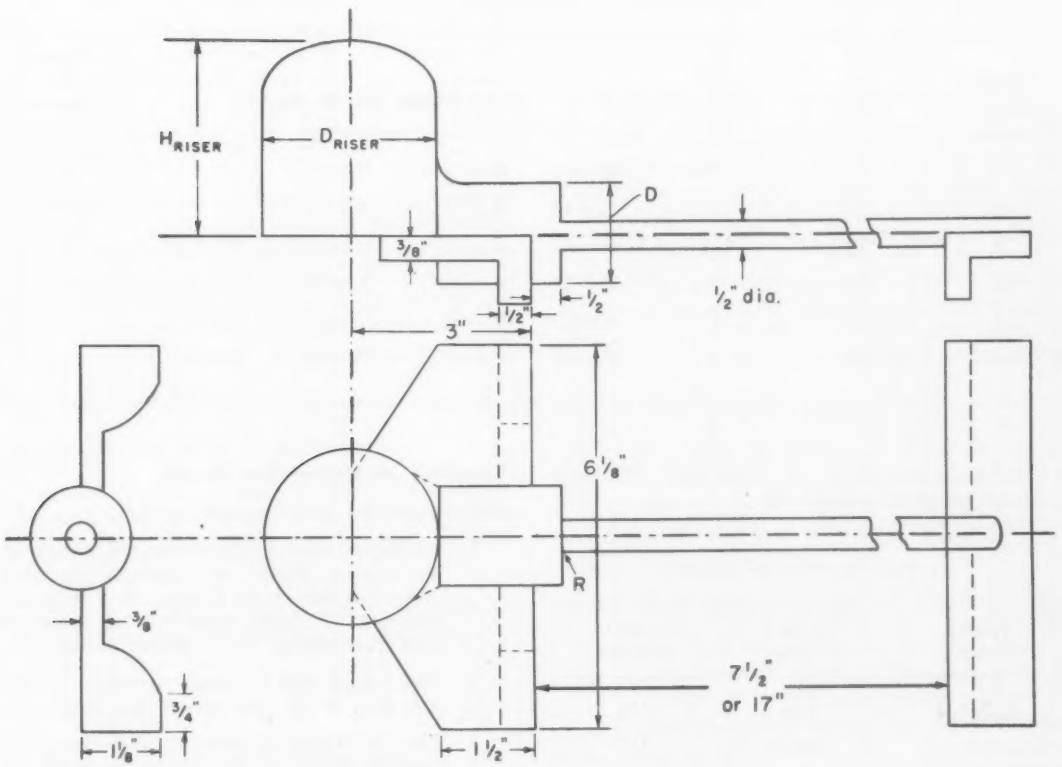


Figure 1—Dimensions of Patterns.

The aim of the first experiments was to measure load at failure under total constraint for varying casting configuration and to determine design influence on tearing characteristics for conditions of delayed constraint.

Loads at failure under total constraint for the castings investigated were too low to be measured with any accuracy for comparative purposes and complete separation of the casting at the section change was obtained in relatively short intervals of time.

As a consequence of the preliminary experiments it was decided to investigate the tearing of castings of various configurations and to select designs which remain untorn for further investigation of the effect of delayed constraint when cast in sand.

Experiments to Survey the Influence of Design on Tearing Behavior

A series of patterns were made of the basic design illustrated in Figure 1. The dimensions of the adjoining sections and the fillet geometry are given in Table I.

Castings in this series of experiments were constrained by the sand alone. The varying constraints applied were are follows:

1. Extension of ½-inch diameter bar to provide 7½-inch gage length between flanges.
2. Extension of ½-inch diameter bar to provide 17-inch gage length between flanges.
3. Removal of extremity flanges from No. 2.

The tearing behavior of the test pieces cast under these conditions was the result of constraint imposed during the initial period of solidification and cooling. It was inferred that the bulk resistance of the sand (for low strength aggregates) contributed little additional constraint, because the tearing of the 17-inch casting with the flange was only slightly more severe than the 17-inch casting without flange. When sands of different green strength were tried, no difference in tearing behavior was detected between a flanged casting made in sand of 4½ psi green strength and a sand of 7½ psi green strength.

TABLE I

Pattern Numbers	d in Inches	D in Inches	FILLET GEOMETRY IN INCHES				Taper in Degrees
1	½	13/16	R=O	R=½	R=1		15
2	½	1	R=O	R=½	R=1		15
3	½	1⅝	R=O	R=½		R=1⅝	15
4	½	2	R=O	R=½	R=1	R=2	15
5	½	3	R=O	R=½	R=1	R=3	
6	½	4	R=O		R=1	R=4	

The dimensions of the risers used with the above patterns are given in Table II.

TABLE II—Riser Dimensions

Pattern Numbers	Diameter In Inches	Diameter In Inches
1, 2, 3, 4	3¼	3¼
5	3½	4½
6	4	4¾

Summary of Section Size Results

CONSTRAINT AT THE HIGHEST LEVEL

1. 15 degree taper castings remained untorn at the section change for diameters up to 2 inches but tore at the flanges. At 2-inch diameter, a 15 degree taper casting began to show fine tearing at the section change.
2. The 1-inch and 1⅝-inch diameter castings tore both at the radii and at the flange.
3. The 13/16-inch diameter castings tore in all cases at the flange, but the tearing decreased at the section change as the radii of the fillet were increased.

4. The 2-inch diameter castings had only fine tears at the flanges, but the radii tore to a diminishing extent up to R=1 inch. The tearing at the radii became more widespread as the radii were increased above 1 inch.

5. The 3-inch and 4-inch castings exhibited no apparent tearing at the flange and the tearing at the section change again decreased for small radii and became more widespread as the radii increased beyond R=1 inch.

6. The 13/16-inch diameter sections were capable of withstanding the total constraint by a fillet geometry of 15 degrees taper; the 1-inch and 1⅛-inch section of 15 degrees taper showed wide tears and the 2-inch section had multiple fine tears.

Constraint At Intermediate Level

A similar pattern of tearing was observed but to a slightly reduced extent. At the 3-inch section change only the finest of tearing was apparent.

Constraint At The Lowest Level

1. Only in the 1-inch castings was tearing noted at the flange, and in the radiused castings in the 1⅛-inch group. In all cases where tearing occurred, the tears were fine and generally were not revealed on the sand blasted casting. Tearing was revealed only after a deep etch.

2. The most pronounced tearing occurred at R=O in the 13/16-inch and 1-inch castings. Of the six 13/16-inch diameter castings when R=O, four showed tears; of the seven 1-inch diameter castings, when R=O, six showed tears. Tearing did not become more pronounced at the section change by chilling at the flange.

Stress Analysis with Plastic Patterns

Several experiments were performed with plastic patterns to determine whether stress analysis might not offer useful information on the effect of design on hot tearing.

With a 15 degree taper, a stress concentration immediately appears on straining at the junction of flange and bar. The stress at the radius and at the flange built up approximately equivalently in radiused castings.

When tearing did occur at the flange in the 15 degree taper casting, stress was relieved at the taper and no further tearing occurred. Tearing occurred simultaneously at the flange and radius in the radiused casting.

Experiments Involving Shape Factors and Stress Systems

Two castings with 1⅝-inch diameter sections and taper of 30 degrees and 60 degrees were made to investigate the influence of angle of taper at the highest level of constraint. Tearing was entirely confined to the section change and no tearing occurred at the flange.

Two castings were made to illustrate the ineffectiveness of increasing the radius at increased ratios of section and the fallacy of the "increased hot spot" concept. For castings cast under the highest level of constraint, the 15 degree taper casting tore at the flange only, while the radiused castings showed tearing along the radius. These castings had approximately equal "volumes of hot spot" but their shapes were different and the tearing characteristics were obviously changed.

Another series of castings indicated that tearing appeared to be initiated at stress raisers and traverse sections.

Experiments Under Total Constraint

These experiments were designed to determine the influence of total constraint on a casting when the contraction was hindered after a period of cooling under sand constraint alone. The following interesting factors emerged from these limited experiments.

1. The local openings around the joint were discontinuous and difficult to adjudge as actual tears. The openings around the grain appeared to be a phenomenon accompanied by necking.

2. It was noted that after deep etching the grain boundaries on the surface of all the castings were deeply etched and where fine tears were observed on the surface the grain boundaries in the vicinity of the fine tears appeared more deeply etched than usual.

3. At the strain rates obtained, the casting tended to deform plastically and tearing might have been initiated at the surface after considerable deformation. The tears were incapable of propagating because the stresses generated are not capable of producing further plastic deformation when the temperature drops below that necessary for recrystallization.

Discussion of the Results of this Study

1. This investigation showed that for the occurrence of hot tearing in a simple joint between two cylindrical bars, relationships do exist between section ratio, constraint, and fillet geometry. Whether the principles herein established can be applied to other casting shapes must be determined by further experiment.

2. For the smallest section ratio cast (13/16-inch by ½-inch), tearing behavior was entirely consistent with predictions from analysis of stress concentration.

3. Tearing occurred at abrupt section changes and could be minimized by use of radii. The degree of strain which the section could withstand without tearing occurring was also a maximum. Also, the delay in build-up of stress at a tapered section could allow sufficient time for solidification to be complete, at which time the section would not tear.

4. The small section change ratio (1-inch: ½-inch) appeared sufficient to allow a time interval in which tearing would occur. Hence for 1-inch : ½-inch ratios, the total strain which the system may withstand without tearing is reduced to a value below that obtained in the 13/16-inch : ½-inch section change. The effect of tapering, however, in these ranges of section ratio and applied stress, may allow the transfer of stress to some other locality where tearing will occur sacrificially.

5. Similar reasoning applies to the 1⅝-inch : ½ inch section change. Also, it indicates that radiusing at this level of constraint has no effect and only a 15 degree tapered section may transfer stresses.

6. At 2 inches, tearing occurs entirely within the system, even with a 15 degree tapered section. Tearing, however, appears to be somewhat diminished in severity for small radii. Increasing the fillet radius leads to more extensive tearing.

7. The severity of tearing was progressively reduced as the section change ratio increased from 3:½ to 4:½ and again, more extensive tearing was noted at large fillet radii.

8. The results for 7½-inch castings indicate that for small section ratios, tearing may be predicted on a basis of stress concentration and prevented by radiusing. The radiused castings tend to tear, but the extent of tearing fell off as the section ratio was increased.

9. The 17-inch castings without flange indicated that constraint must be almost entirely friction. It may be considered, therefore, that:

   a. All the observed tearing has taken place during the earliest part of solidification and contraction.

   b. For large diameter castings, tearing may be prevented if strain vanishes during the solidification interval.

   The latter statement is well understood by foundrymen and is applied, for example, in the rapid collapsibility of cores.

10. It also appeared that under the experimental conditions, little additional constraint was applied by the flange, and frictional resistance as opposed to bulk resistance of the sand was the dominating factor.

11. The depth of tearing bears a relationship to the ratio of diameters of the section change.

12. At 2 inches, which corresponds to the peak of tearing severity, the location of the tears tends to become displaced internally for smaller radii.

Design Recommendations

The observations which have been made enable a few tentative recommendations to be made for design. These hold for the ratios of section and for the conditions of testing obtaining in the experiments performed. Theoretically, they should apply only to joined round bars and the possibility of extending the treatment to joined bars of larger dimensions remains to be verified experimentally.

1. For Section Ratios Up To 2 : 1

Radiusing is effective in overcoming hot tearing when strains are small. A radius of 1 inch was found by experiment to be most suitable.

For higher strains, tapering at 15 degrees is most effective but when this is practiced, the location of tearing is displaced from the section change to the next sharp section change in the direction of the heat center. Tearing may thus be displaced from the casting members to the gate or riser area.

(continued on page 15)

THE SIMPLIFIED METHOD USED TO RISER A TEN-INCH PIPE LINE VALVE

*By Alan G. Linley**

The 10-inch pipe line valve casting shown in Figure 1 is suitable for use with either water, oil or gas. It is to be tested hydrostatically with water at a pressure of 1500 psi. The 16¾-inch diameter end and the 11⅞-inch diameter port areas must be radiographically sound. The rest of the body must be produced to pass ASTM-E71 Class (2) Radiographic Standard.

Figure 2 is a photograph of the casting after shakeout, and Figure 3 is a photograph of the core used to produce this casting.

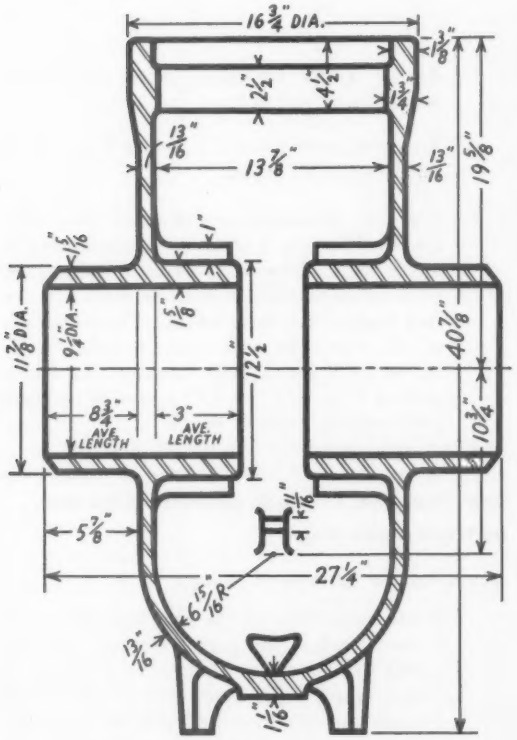


Figure 1—Ten-inch pipe line valve

The necessary calculations required to determine the number and dimensions of the risers required to produce this valve casting to the radiographic requirements are based on the procedures described in Volumes 1 and 2 of the Journal of Steel Castings

Figure 2—Ten-inch pipe line valve after shakeout showing risers and gate

Figure 3—Core used in producing ten-inch valve

* Metallurgist, Locomotive Finish Materials, Division of Rockwell Manufacturing Company

Research, and the SFSA Research Report No. 30, "Determination of the Factors Influencing Riser Efficiency—Part II—The Distance Risers will Feed Uniformaly Thick Sections". The necessary figures from the Journal of Steel Castings Research are reproduced for the readers convenience.

This method of riser calculation is limited to a parent section with attached appendages. Thus, this valve is divided into three separate units, namely: (1) the 16⅞-inch diameter end with the 1¾-inch by 2½-inch heavy section, (2) the 11⅞-inch diameter weld end, port and appendage and (3) the bowl end of the valve.

Unit One—The 16⅞-inch diameter open-end.

Consider the 1¾ x 2½ inch section as the riser for the lighter appendages. Thus, this section is the parent section.

Determining The Number of Risers

1. The median circumference of the 1¾ x 2½-inch section is $\pi(16.75-1.75)=47.12$ inches.
2. Assume that 4 risers with 4.5-inch contacts are required.
3. Feeding distance required of each riser is $\frac{47.1''-18''}{8}=3.6$ inches.
4. According to Research Project No. 30, a riser will feed a plate a distance of 2T or $2 \times 1.75''=3.5$ inches.
5. Thus, this section can be fed sound with four risers only if the contact areas are increased to 19 inches total length.

Calculating Riser Dimension

1. The volume of ¼ of the parent section is $\frac{2.5'' \times 1.75'' \times 47.12''}{4}$ or $\frac{206}{4}$ cubic inches or 51.5 cubic inches.
2. Volume of ¼ of the 1⅜-inch bar appendage, whose median circumference is 48.3 inches, is equal to $\frac{2'' \times 1\frac{3}{8}'' \times 48.3''}{4}$ or 33.2 cubic inches.
   a. Appendage volume to be added to parent volume is determined by the ratio of $\frac{\text{T of appendage bar}}{\text{T of parent bar}}$ or $\frac{1.375''}{1.75''}=0.79$
   b. From Figure 4, it is observed that 0.8 of the appendage bar volume is added or 26.5 cubic inch.
3. Volume of ¼ of the $\frac{13}{16}$-inch plate appendage is equal to ¼ (0.8125" x 4.875" x 46.1") equals 45.6 cubic inches.
   a. The width of the appendage is determined from the fact that the riser can feed a distance of 6T and obtain the soundness requirements specified. Thus, $6 \times \frac{13}{16}'' = 4\frac{7}{8}$ inches.
   b. Appendage volume to be added to parent volume is $\frac{\text{T of appendage plate}}{\text{T of parent bar}}$ or $\frac{0.8125}{1.75}=0.47$. Therefore, from the Bar-Plate curve (Figure 4), the volume to be added equals 0.80 of the volume of the plate appendage or 0.80 x 45.6" equals 36.5 cubic inches.
4. Sharp Factor equals $\frac{L+W}{T}=11.5+\frac{2.5}{1.75}=8$.
5. R_V/C_V (from Figure 5) using 10 percent safety factor = 0.6.
6. Riser volume = 0.6 (51.5 + 26.5 + 36.5) equals 69 cubic inches.
7. The riser dimensions are obtained from Figure 6. They are 5 inches in diameter and 4 inches high. Risers were made 5 inches high by 5 inches in diameter because risers will not feed higher than their height. Therefore each of the four risers were made 5 inches in diameter by 5 inches high (above neck) and 4 chills 0.7" x 1.75" or 1.25 inches thick, were used halfway between risers.

Unit Two—The 11⅞-inch diameter weld end, port and appendage.

Determining Length of Parent Bar

If the intersection of the port and valve body is considered as the parent section, then the parent bar is a square 3 inches by 3 inches. A 3-inch square bar is used as the parent section because the inscribed hot spot of the port-body intersection is a circle 3 inches in diameter and the nearest shape to this cylinder is a square bar 3 inches on a side. This intersection is a complex shape, and its length cannot be determined from Figure 1. Its length is determined by measuring the intersection on the pattern.

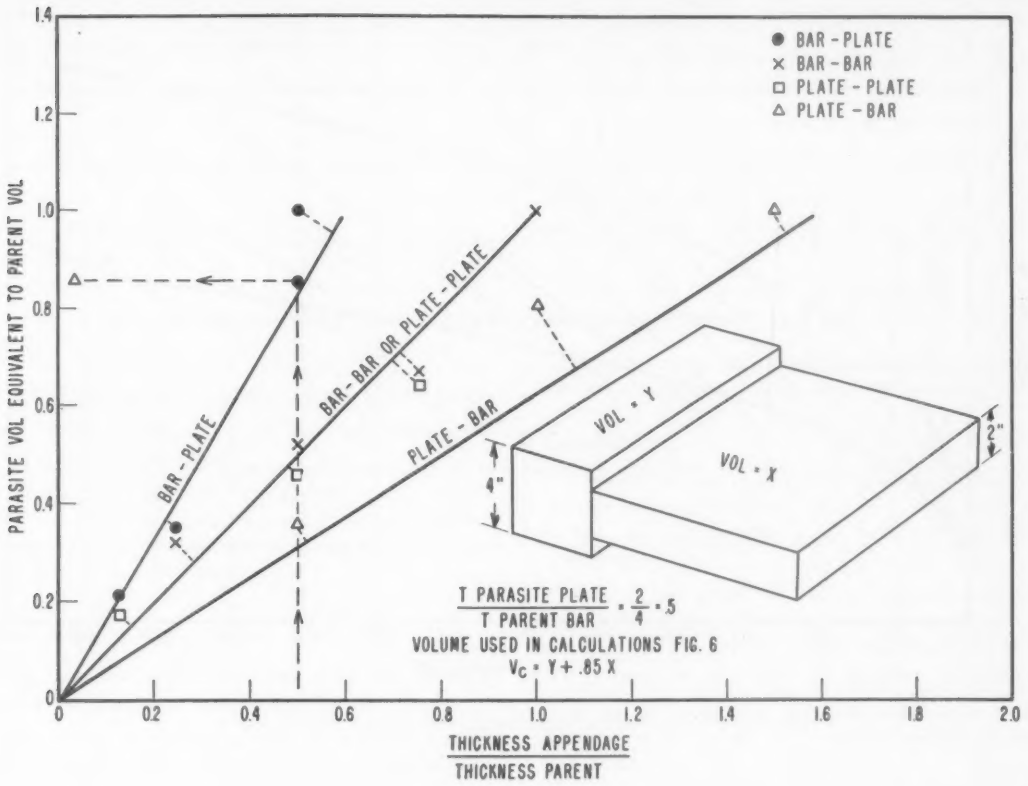

BAR - PLATE
BAR - BAR
PLATE - PLATE
PLATE - BAR
PARASITE VOL EQUIVALENT TO PARENT VOL
BAR - PLATE
BAR - BAR OR PLATE - PLATE
PLATE - BAR
VOL = Y
VOL = X
4"
2"
T PARASITE PLATE / T PARENT BAR = 2/4 = .5
VOLUME USED IN CALCULATIONS FIG. 6
Vc = Y + .85 X
THICKNESS APPENDAGE / THICKNESS PARENT

Figure 4—Percentage of appendage volume to be added to the parent volume to determine the minimum riser volume

Feeding Distance

Research Report No. 30 states that the distance a riser will feed a square bar with an end-effect is $6\sqrt{T}$ or $6\sqrt{3}=10.4$ inches. The end-effect is produced by core and mold chills at the center of the hot spot. The core chills are shown in Figure 3. The median circumference at the spot is 36 inches. As two risers will be used, the feeding distance required on each side of the riser is $\frac{36}{4}=9$ inches. Thus, two risers will adequately feed the parent section.

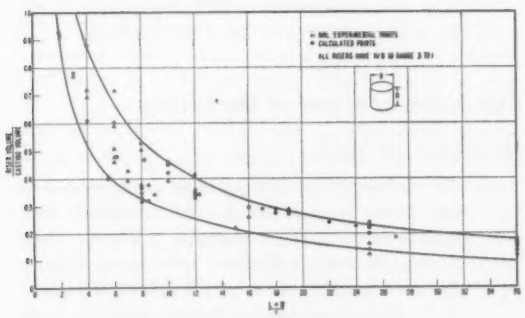


Figure 5—Relation of casting shape factor to minimum effective riser volume as a fraction of casting volume

Riser Calculations

1. Volume of parent bar is 3″ x 3″ x 18″ = 162 cubic inches.
2. Volume of ½ of the 1⅝-inch thick appendage $=\pi\left\{\frac{(6\frac{1}{4})^2-(4\frac{5}{8})^2}{2}\right\}\times 3=83.3$ cubic inches.

   a. $\frac{\text{T appendage bar}}{\text{T parent bar}}=\frac{1.625}{3}=0.54.$

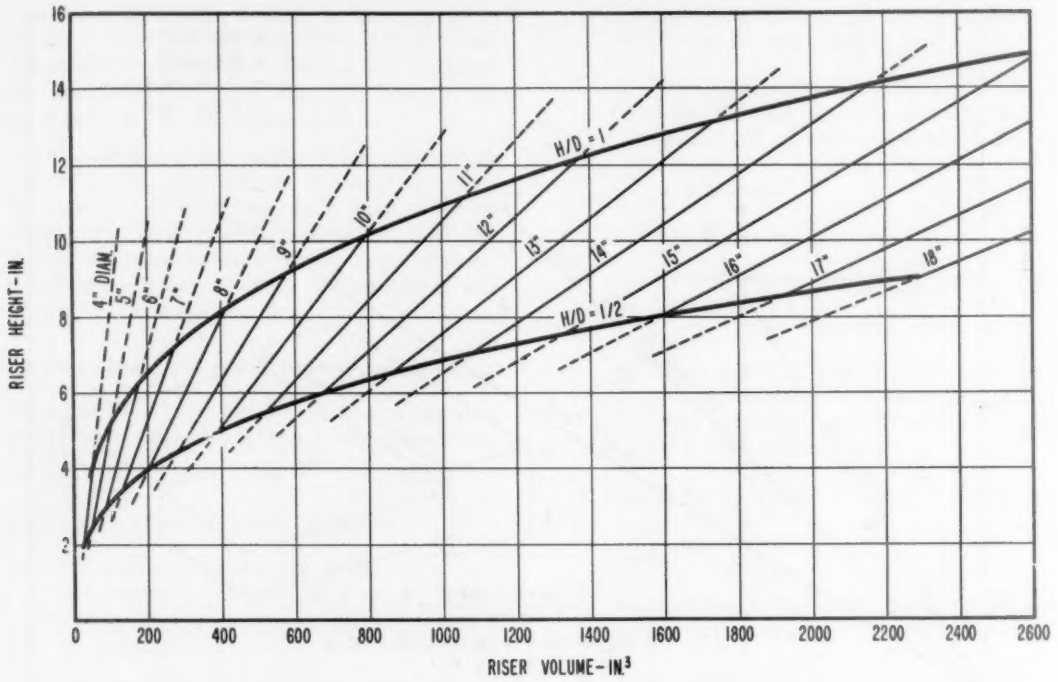


Figure 6—Chart for conversion of required riser volume to riser dimensions

b. Percentage of appendage bar volume to be added as appendage volume is 0.54 (From Figure 4) or 45 cubic inches.

3. Volume of $\frac{13}{16}$-inch appendage is $\frac{\pi d}{2}$ x 6T x 0.81 or 24.4 x 4.8 x 0.81 = 95 cubic inches.

a. $\frac{\text{T appendage plate}}{\text{T parent bar}} = \frac{0.81}{3} = 0.27$

b. Percentage of appendage plate volume to be added as appendage volume is 0.48 (From Figure 4) or 45.8 cubic inches.

4. Volume of $1\frac{5}{16}$-inch thick appendage is $\pi \left\{ \frac{(5\frac{1}{16})^2 - (4\frac{5}{8})^2}{2} \right\}$ x 8.75" = 190.5 cubic inches.

a. $\frac{\text{T appendage plate}}{\text{T parent bar}} = \frac{1.3125}{3} = 0.44$

b. Percentage of appendage plate volume to be added as appendage volume is 0.70 (From Figure 4) or 133.4 cubic inches.

5. The shape factor of the parent bar is $\frac{L + W}{T} = \frac{18 + 3}{3} = 7$

6. R_V/C_V plus 10 percent safety factor equals 0.61 (From Figure 5)

7. Riser volume = 0.61 (162 + 45 + 46 + 133) = 234 cubic inches

8. Riser dimensions obtained from Figure 6 are 7 inches in diameter by 7 inches high.

Unit 3—The bowl end of the valve.

DIMENSION OF PLATE

The bowl end of the valve is considered as a flat plate $\frac{13}{16}$-inch thick with a center riser which has a 4-inch diameter contact. The riser will need sufficient volume to feed a distance of 6T or 6 x 0.8125 = 4.9 inches. If it is assumed that a 4-inch diameter riser is required then the diameter of the $\frac{13}{16}$-inch plate is 13.8 inches.

RISER CALCULATIONS

1. Volume of plate equals
$\pi \frac{(13.8)^2}{4} \times 0.8125 = 121$ cubic inches.

2. Shape Factor $\frac{L + W}{T} = \frac{13\frac{3}{4} + 13\frac{3}{4}}{1\frac{3}{16}} = 33.8$

3. $R_V/C_V = 0.2$ (From Figure 5)

4. Riser volume equals 121 x 0.2 = 24 cubic inches

5. Riser dimensions obtained from Figure 6 are 4-inches in diameter by 4-inches high.

The legs and center boss on the end were externally chilled. The cope risers were poured to full cope height to provide metal for the system before the riser systems are isolated by the chills which also provide the establishment of end effects.

Research Report No. 41 (continued from page 10)

2. FOR SECTION RATIOS 3 : 1

At small strains, a small radius of less than ½ inch is recommended. At ½ inch and above, fine tearing is likely to occur.

At large strains, only a 15 degree taper is effective. In this instance, tearing will again be displaced in the direction of the heat center and may be transferred to a riser or gate area.

3. FOR SECTION RATIOS 4 : 1

At small strains, a small radius of less than ½ inch is recommended as for No. 2.

For larger strains, no geometrical configuration will prevent tearing within the area of the section change. Tearing may be prevented by reducing strains. Radii of ½ inch are recommended.

General

For any section change, when large strains are totally imposed, a section ratio of 3 : 2 is most effective in resisting the strain with suitable fillet geometry.

Between the ratios 2:1 and 4:1, the size factor is most unfavorable to total strain accommodation and strains must be correspondingly decreased. However, stresses may be transferred as described in 1 and 2.

At the ratio 4:1, the permissible total strain again increases progressively with further increase of section ratio.

THE INFLUENCE OF SULPHUR AND PHOSPHORUS ON THE MECHANICAL PROPERTIES OF HEAT TREATED LOW AND MEDIUM ALLOY STEEL CASTINGS.*

This study was carried out as a sequel to previous investigations of the influence of sulphur and phosphorus on the tensile properties of acid and basic carbon steels. The effect of sulphur and phosphorus on the maximum impact strength of low and medium alloy steel castings was investigated by applying statistical methods of analysis to a limited amount of Charpy V-notch test data. The results show that for steels tempered above 1020 degrees F to a hardness of 230 (± 15) Brinell the maximum impact value (100 percent fibrous) tends to decrease as the sulphur content increases, whereas phosphorous does not influence the maximum impact value. For steels tempered below 1020 degrees F however, the impact values in room temperature tests of the "high-phosphorus" materials were less than those of the "high-sulphur" material. The latter effect was found to be the result of the more pronounced effect of phosphorus in raising the transition temperature.

Further analysis revealed a definite relationship between maximum impact values (100 percent fibrous) and the work to fracture as computed from the area under the load/extension curve of the tensile test. This relationship was not noted in previous work because room temperature test values were evidently considered rather than the maximum impact values. It was also shown that sulphur tends to lower the tensile ductility (percent elongation); phosphorus exhibited a similar trend but with lower statistical assurance.

*Abstract—By W. J. Johnson, M. Sc., ARIC, AIM Published by BSCRA.

MASS PRODUCTION OF MOLDS AND CORES WITH THE CARBON DIOXIDE PROCESS

Many papers published in the past few years on the carbon dioxide molding process have been devoted to the mechanics, advantages and disadvantages of the process. A review of the translations of papers recently published in Germany [1] [2] however, reveals some interesting observations and experiences with the use of the process on a mass production basis. According to the authors, the process can be used economically for making cores and molds without using special equipment but the inherent advantages of the process can best be exploited by using machinery specially designed for use with the process.

The successful application of a machine designed for the use of the carbon dioxide process is illustrated by the experience of the author with a large circular platform for making mold sections for a lost flask, multiple-mold set up. The platform incorporates four stations: (1) sand blowing or filling; (2) jolting or vibrating; (3) carbon dioxide injection, and (4) mold removal. The sand injection heads at each station can be interchanged or arranged so that one revolution of the platform yields any combination of bottom and intermediate mold sections for the same or different patterns. Thus, molds for two or three jobs can be made on the same machine. The author [1] reports that 1200 complete molds ready for pouring can be produced on this machine by one operator in an 8 hour day.

Molds were successfully produced using the waterglass sand mix as a facing, either manually packed or mechanically blown into the mold and backed up with normal backing sand. The carbon dioxide was then injected either through the bottom platform of the molding machine or manually through vent holes penetrating the sand to the pattern. The drying time saved by the carbon dioxide hardening of the facing proved economical even for the manually rammed molds.

The waterglass/carbon dioxide process lends itself particularly well to coremaking. This is easily understood when it is realized that the time required for handling and drying oil sand cores is very much greater than the time required to blow the cores. The carbon dioxide process, on the other hand, involves only the time necessary to blow the cores and inject the hardening carbon dioxide. Increased production and lower costs were realized even when the process was used in the normal coremaking operation.

By redesigning a core blowing machine to facilitate the injection of carbon dioxide through the blowing head and incorporating eight small cores per core box in a stellate arrangement, in place of one per box, a German foundry [2] was able to produce the same number of cores with one machine and one operator as was previously produced by four operators and four machines. This increase in production does not take into account the time saved by eliminating the drying process. The fact that the cores were completely hardened when they were removed from the core box enables the operator to break the individual cores from the aggregate with light blows. An improvement in dimensional control was also realized with the use of carbon dioxide hardened cores. This, of course, is a result of the cores being entirely hardened while they are still in the core box.

Hollow cores were successfully and economically produced by both the shell method and by using special equipment to produce hollow cores directly. For short run jobs, hollow cores were produced directly by extracting a cylinder of sand from the filled core box prior to hardening, while a special core blowing machine was used for jobs of long duration.

A stainless steel cylinder was used to extract the sand from the core box on the short run jobs, with carbon dioxide then being injected into the resulting cavity. The machine used for jobs of long duration included a sand blowing head with a perforated cylindrical tube extending into the core box, a pneumatic vibrating platform and a carbon dioxide feed to the perforated tube. The sand mix was blown into the core box around the perforated tube and the platform vibrated to insure complete filling and to keep the sand from encrusting around the tube; carbon dioxide was then fed into the tube and through the sand with the sand filling port closed.

Thus the authors show that the carbon dioxide process can be used to produce molds and cores on a mass production basis to realize marked increases in production and lowering of costs.

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1. Saubermann, W., Giesserei-Praxis, Vol. 73, No. 2 pp. 29-32 and No. 3, pp. 49-51 (1955) (Henry Brutcher Translation No. 3409).
2. Gerstmann, O., and Grohmann, E., Giessereitechnik, Vol. 1, No. 2, pp. 16-17 (1955) (Henry Brutcher Translation No. 3514).